

Nozzleless Booster Performance Study

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DREV

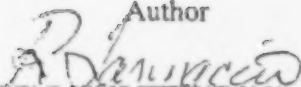
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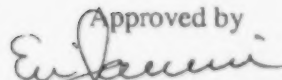
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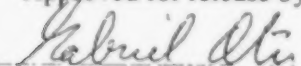
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Abstract

A formulation based on HTPB/AP propellant with FEOOH/CuPht co-catalysts was developed as a potential candidate for use as a nozzleless booster propellant. This propellant exhibited qualities of high burn rate and good combustion stability with high mass flow necessary to cause exhaust gases to choke downstream in a cylindrical grain. Another consideration was that the pressure exponent was low enough to give a more uniform burn rate across a larger pressure regime. The propellant exhibited a burn rate of 30 mm/s at 6.89 MPa and a pressure exponent of 0.412. Tests of experimental motors have shown that the pressure and thrust are dependent on the ratio of the motors length to diameter (L/D). A prediction model yielded similar results as those obtained experimentally. However, it was shown that erosion effects and non-uniform burn rates along the axial length affect the overall ballistic performance and that the prediction model lacks these effects. The 1-D model, in most part, overestimates the performance by omitting the 2D complexity of the problem.

Résumé

On a mis au point une composition basée sur le propergol HTPB/AP et des cocatalyseurs FEOOH/CuPht. Il s'agit d'un produit d'intérêt potentiel pour utilisation comme combustible de propulseurs sans tuyère. En raison de sa vitesse de combustion élevée, sa bonne stabilité de combustion et son débit massique élevé, les gaz d'échappement produisent un effet d'étranglement en aval dans un grain cylindrique. Il faut également s'assurer que l'exposant de pression est suffisamment bas pour générer une vitesse de combustion uniforme à une pression plus élevée. On a enregistré une vitesse de combustion de 30 mm/s à 6,89 MPa et un exposant de pression de 0,412. Les essais effectués sur des moteurs expérimentaux ont démontré que la pression et la poussée dépendent du rapport longueur/diamètre (L/D) du moteur. Un modèle prédictif a permis d'obtenir des résultats semblables à ceux enregistrés. On a toutefois démontré que les effets de l'érosion et une vitesse de combustion non uniforme sur la longueur axiale affectait la performance balistique globale, ce qui n'est pas le cas du modèle prédictif. Le modèle 1D surestime en grande partie cette performance, car il ne tient pas compte de la complexité 2D du problème.

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Executive summary

Future tactical missiles for use in the military arsenal have to meet requirements that encompass an operational envelope that is larger than what they are today. Flight distance of air-air and surface-to-air missiles will be extended in order to decrease the detection and increase the survivability of the launch platform. Ramjet or ducted rockets would meet this requirement. These air breathing rocket motors operate on a principle that their velocity can be sustained by throttling the solid fuel and that it can achieve distances and performances that are superior than existing solid propellant rocket motors. The rocket ramjet, however, requires an initial boost that would accelerate the missile to an appropriate speed so that the air (oxidizer) and fuel could mix and burn efficiently. The boost of such a system would necessitate a solid rocket propellant booster. This booster can be constructed with or without a nozzle. The nozzleless booster would involve ejecting the combustion products of an average burn rate propellant through a nozzle. Upon the complete burning of this propellant, the nozzle would then be ejected and the combustion of the ramjet would be initiated. The other design implies that no nozzle be used, but rather a higher burn rate propellant that would be cast with a bore that acts as its own nozzle. This propellant would burn more quickly and would have a bore that would accelerate the mass flow of the combusted material. A nozzleless booster has advantages over the nozzleed booster, in that it is mechanically simpler and cheaper. It also is a safer method of boosting the ramjet. The dispersal of material from an ejected nozzle may be a hazard to the launch platform, whereas the nozzleless booster would have no ejected material.

The work presented in this report describes the development of a high burn rate propellant and the experimental performance evaluation of the nozzleless booster using this propellant. It was found that the addition of certain catalysts in the chemical make-up of the propellant helped increase the burn rate of the propellant. However, the burn rate was highly dependent on the operating chamber pressure. This is not desirable for the operation of an efficient nozzleless booster. The burn rate of the booster should be less dependent on the operating pressure (i.e. a lower pressure exponent). This limitation implies that the burn rate would be much lower than anticipated. The final propellant formulation developed in this program was a compromise between the higher pressure dependency and the lower burn rate. This propellant was then used in configured rocket motors where the performance elements such as thrust and pressure were measured. The result of these tests shows that, for motors with the same diameter, there is a performance dependency on the ratio of L/D (length to diameter) of the motor. Therefore, higher L/D develops higher thrust. Optimally, one is limited by the geometry of the rocket motor.

The predictions from a 1D model were also compared with the test results. It was found that the trends between the model and the experiments were similar. However, the peak performance values of the model such as chamber pressure and thrust were higher than those obtained from the tests. Hence, the simplistic nature of the model needs to be altered to include some elements, such as erosive burning, that are perceived important for modelling performance of the nozzleless motor.

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Sommaire

Les futurs missiles tactiques de l'arsenal militaire devront satisfaire des critères supérieurs de fonctionnement, c'est-à-dire supérieurs à ceux d'aujourd'hui. La plus grande distance parcourue par les missiles air-air et sol-air permettra de réduire les risques de détection et accroître la surviabilité des plates-formes de lancement. Les statoréacteurs ou statofusées satisferaient ces critères. Ces moteurs-fusées aérobies fonctionnent selon le principe que leur vitesse peut être atteinte en contrôlant l'arrivée du combustible solide. La distance parcourue et la performance peuvent être supérieures à celles des actuels moteurs-fusées à combustible solide. La statofusée nécessite toutefois une poussée initiale qui permet au missile d'atteindre une vitesse suffisante pour que l'air (oxydant) et le combustible puissent se mélanger et brûler efficacement. La poussée d'un tel système nécessiterait un propulseur à propergol solide avec ou sans tuyère. L'utilisation d'un propulseur avec tuyère implique l'éjection des produits de combustion d'un propergol à vitesse de combustion moyenne par une tuyère. Après la combustion complète de ce propergol, la tuyère se détacherait et la combustion du statoréacteur serait amorcée. L'autre option suppose de ne pas utiliser de tuyère, mais un propergol à vitesse de combustion élevée qui serait coulé de façon à ce que l'alésage joue le rôle de la tuyère. Le propergol brûlerait plus rapidement et son alésage accélérerait le débit massique du matériel brûlé. Un propulseur sans tuyère possède certains avantages par rapport à celui avec tuyère; sa mécanique est moins complexe et il coûte moins cher. C'est une façon plus sécuritaire de propulser le statoréacteur. Les produits d'échappement d'une tuyère peuvent être dangereux pour la plate-forme de lancement. Par contre, aucun produit ne s'échappe d'un propulseur sans tuyère.

Dans ce rapport, on décrit la mise au point d'un propergol à vitesse de combustion élevée et on évalue la performance expérimentale d'un propulseur sans tuyère qui utilise ce combustible. On a ainsi découvert que l'ajout de certains catalyseurs à la composition chimique du propergol permettait d'accroître son taux de combustion. Le taux de combustion dépend toutefois en grande partie de la pression de fonctionnement de la chambre, ce qui n'est pas souhaitable pour le bon fonctionnement d'un propulseur sans tuyère. La vitesse de combustion du propulseur devrait moins dépendre de la pression de fonctionnement (c.-à-d. un exposant moins élevé de pression). Cette contrainte suppose que le taux de combustion serait beaucoup plus bas que prévu. La composition finale du propergol mis au point dans le cadre de ce programme représente un compromis entre une pression plus élevée et une vitesse de combustion moins élevée. Ce propergol a déjà été utilisé dans les moteurs-fusées élaborés dont on a évalué les critères de performance, comme la poussée et la pression. Les essais ont permis de démontrer que chez les modèles dont le diamètre est semblable, la performance dépend du rapport L/D (longueur/diamètre) du moteur. Par conséquent, un rapport /D plus élevé permet d'obtenir une plus forte poussée. Dans le meilleur des cas, la géométrie du moteur représente une contrainte.

On a également comparé les prédictions obtenues à l'aide d'un modèle 1D aux résultats de l'essai. On a ainsi découvert que les tendances observées sur le modèle et lors des expériences étaient semblables. Toutefois, les valeurs de rendement maximum observées sur le modèle, comme la pression de la chambre et la poussée, étaient supérieures à celles obtenues lors des essais. Il est donc nécessaire de modifier la nature peu complexe du modèle pour y inclure certains éléments, comme la combustion érosive, jugés importants dans la modélisation de la performance du moteur sans tuyère.

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1.0 INTRODUCTION

The development of high burn rate propellants to be use in solid rocket motors has spurred the use of nozzleless boosters as a method for accelerating ramjet rocket motors. The booster motor to accelerate the missile to a velocity where the flow through the intake of a ducted or ramjet motor could sustain the combustion of the propellant in the combustion chamber of the motor. The use of a nozzleless booster on air-launched platforms is a safe alternative to nozzle boosters. In most tactical ramjet or ducted rocket missile systems, the booster incorporates a fixed nozzle with a propellant that burns at constant pressure and constant burn rate. An ammonium perchlorate/ hydroxy-terminated polybutadiene binder (AP/HTPB) solid propellant is normally used for the boost mode on such missile systems. After the boost propellant has been expended, the nozzle is ejected and the combustor of the ramjet is engaged. The booster segment then acts as a mixing and combustion chamber for the subsonic airflow and the throttled solid fuel. The required choke area for the fuel/air mixture is larger than those used for solid rocket propellant due to a lower operating pressure and lower propellant mass density. Therefore, in this application, the ejection of the nozzle would increase the likelihood of damage to the launch platform due to backward scatter of the material being ejected. The use of the nozzleless booster avoids the material scatter by employing the bore diameter of the high burn rate propellant as a choked nozzle. One of the more significant challenges is to produce a propellant with a high enough burn rate to cause the exhaust gases to choke at the exit of the booster for a given length-to-bore ratio. To be applicable to nozzleless boosters, a solid propellant should combine a high volumetric specific impulse and a high burn rate with good combustion stability. If reduced visibility is desired, a low signature propellant must be selected. Under the current program, it was decided to use a low-smoke propellant formulation based on ammonium perchlorate (AP) and an inert binder. Building on a previous experiment at Defence Research Establishment Valcartier (DREV), the binder is composed of the well-known R-45M hydroxyl-terminated polybutadiene (HTPB) mixed with a plasticizer (isodecyl pelargonate, IDP) and cured with dimeryl diisocyanate (DDI). Bonding agents are also added to improve mechanical properties.

A project was initiated at DREV to investigate the use of high burn rate propellants and establish a test series of motor firings to measure the performance of the nozzleless booster. Previous work by Gany et al. has shown that the performance is dependent on nozzleless motor characteristics, such as, chamber pressure, motor length, total diameter and bore diameter of the propellant (Ref. 1). The work performed in the DREV study looks at the these parameters on the developed propellant. Further to this, a one-dimensional interior ballistics model was developed to calculate performance parameters based on initial designs and to compare the model to the experimental results. This work is also an extension of the work presented in an AIAA proceeding (Ref. 2)

2.0 Propellant formulation and processing procedures

2.1 Formulation

The development of a High Burn Rate (HBR) propellant for use in nozzleless boosters entails detailed ingredient selection and complex matrix testing (Ref. 3). Given that resources associated with such testing would be great, it was decided that we would take a more restrictive approach to its development. As a starting point, an AP/HTPB propellant formulation, developed in a previous project with Bristol Aerospace (Ref. 3) on HBR propellants, was selected. The reference formulation (HBR-130) had a solid loading of 87.5%, consisting of a blend of 55% 200 μm and 45% 6.7 μm AP, 1% hydrated ferric oxide (FeOOH) as burn rate catalyst and 1% flake aluminum (Al) to control combustion stability. In the previous program, this formulation showed a nominal burn rate (r_{b1000}) at 6.89 MPa (1000 psia) of 25 mm/s and a pressure exponent (n) of 0.375.

Since it was felt necessary to further increase the burn rate, the approach taken was to keep the Al and FeOOH level constant at 1% and use a finer 3.5 μm AP particle size. Because of the finer solids, higher viscosities were expected. But in order to achieve pour-castable formulations, the end-of-mix (EOM) viscosity at the casting temperature should be between 6 and 10 kilopoises (kP) and the pot-life should be long enough to allow time to eventually load the composition into rocket motors. To prevent eventual processing problems, work thus started with a modified formulation using a slightly lower solid content (87%) and a lower fine content (40% of blend). The strategy to include slightly higher fines and solid content in subsequent batches was adopted.

2.2 Processing

All propellants were processed either in a Helicone 4 CV (capacity 0.5 US gal) or an 8 CV Mixer (capacity 2 US gal) from Atlantic Research Corporation. Both mixers are equipped with vacuum lines and a drive-mounted torque meter that is used to assess processing viscosities (Ref. 4). Propellant samples and motors were cast under vacuum.

The rate of cure were obtained by measuring the viscosity of propellant samples using a Haake Rotovisko viscosimeter coupled with special controls for continuous recording of the torque and automatic retraction of the viscosimeter probe. All measurements were performed with a Brookfield type D spindle at the casting temperature of 60°C. The initial rate of cure was later determined by evaluating the average slope for the first three hours on the viscosity-time curve.

Propellants samples and motors were cured in an oven at 60°C. The hardness of a witness block was measured using a "Shore A" durometer until the same value (end of cure hardness) was obtained for two consecutive days. In most cases, 7-8 days were required for the curing reaction to be completed.

3.0 STATIC FIRING SETUP

The development work also encompassed a study of the selected propellant in a motor configuration. Nozzleless booster test cylinders with various length-to-diameter motor configurations were designed and fabricated. These assessed the performance of the motors and established a criterion for optimal design. These test motors were 70 mm in diameter (propellant diameter $D=63.5$ mm) and varied in length from an L/D of 5 to 11. Since the motor is governed by the aerodynamic constriction of the gas flow, the bore geometry of the propellant was to function like the throat of a nozzle motor. The propellant grain was cylindrical in nature and was designed with an exit cone, at the aft end, which allows gas expansion. The exit cone was similar in shape as a nozzle motor, i.e. angle of expansion was about 20° .

The bore diameters for each motor were calculated using the burning rate properties and an analytical internal ballistic 1-D model presented in a previous report (Ref. 6). The physical 1-D equations used were from Gany et al (Ref. 1). The theoretical analysis consisted in deriving equations for a quasi-steady-state one-dimensional flow of a perfect gas in a constant area port. It was assumed that the physical gas properties of the combusting material were constant and a uniform burn rate along the grain existed for the propellant. A concentric cylindrical bore within the propellant represented the constant area port in the model. The model was used to calculate the ballistic characteristics of the booster with respect to the vehicle physical requirements, such as weight, length, diameter, etc. The calculations do not include ignition, tail-off and erosion models. Both ignition and tail-off are represented as instantaneous events within the calculation. The bore diameters were chosen arbitrarily to give pressure ranges from 6000 to 14000 kPa. This would provide a large range of data points and provide insight in limitations and dependencies of the geometries studied.

Table 1 and Figure 1 describe the propellant geometry for each motor tested. The motor casings were made from thick walled, mild steel reusable cylinders. Each cylinder was lined internally with an insulating material to avoid premature burning of the propellant at the case wall and also to prevent gas venting through instrument openings. The drawings for each of these cylinders are shown in Annex A. A head igniter system was used to ignite the propellant. This is shown in Figure 1 and Annex A. The head-end pressure along with the motor thrust was measured for each test. Five (5) Kistler pressure gauges evenly spaced along the length of the motor measured static pressure as a function of length and time of burn. The first gauge was placed at approximately 6 cm from the head end (referred to as head-end pressure), the second at about one-third of the length, a third at half of the length, a fourth at about two-thirds of the length and the fifth at 6 cm from the presumed nozzle end (referred to as nozzle-end pressure). These are also shown in the drawings in Annex A. Thermocouples (Type K) were imbedded in the propellant at the propellant/case interface, one-third and two-thirds of the propellant web thickness at both the head and nozzle-ends to measure flame front temperature and to determine the time to burn to the depths at these locations. An Endeeco conditioner and Labview data acquisition system were used for the tests. Sampling time was set to 1 KHz.

Table 1. Motor test configuration

MOTOR NO.	BORE DIAMETER (mm)	WEB THICKNESS (mm)	PROPELLANT LENGTH (mm)	L/D	PROPELLANT WEIGHT (kg)
8038	12	25.7	317.5	5	1.4967
8039	16	23.7	444.5	7	2.0499
8040	22	20.7	571.5	9	2.5337
8045	12	25.7	381	6	1.8124
8046	16	23.7	508	8	2.4042
8047	22	20.7	635	10	2.8132
8048	26	18.7	698.5	11	2.8944

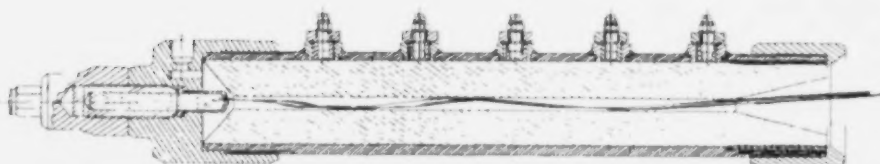


Figure 1 – Example of grain and motor

Figure 2 shows the test setup of the motor in the vertical thrust test stand at the Propulsion Laboratory. The Baldwin gauges, which measure the head end pressure, can be seen with the Kistler ports along the length of the motor and the thermocouple rods located at the head and nozzle ends. A thin paper sheet can also be seen covering the nozzle end. This acts as a barrier for initial high ignition pressure and constant volume within the motor when igniting and prevents venting of the ignition gases.

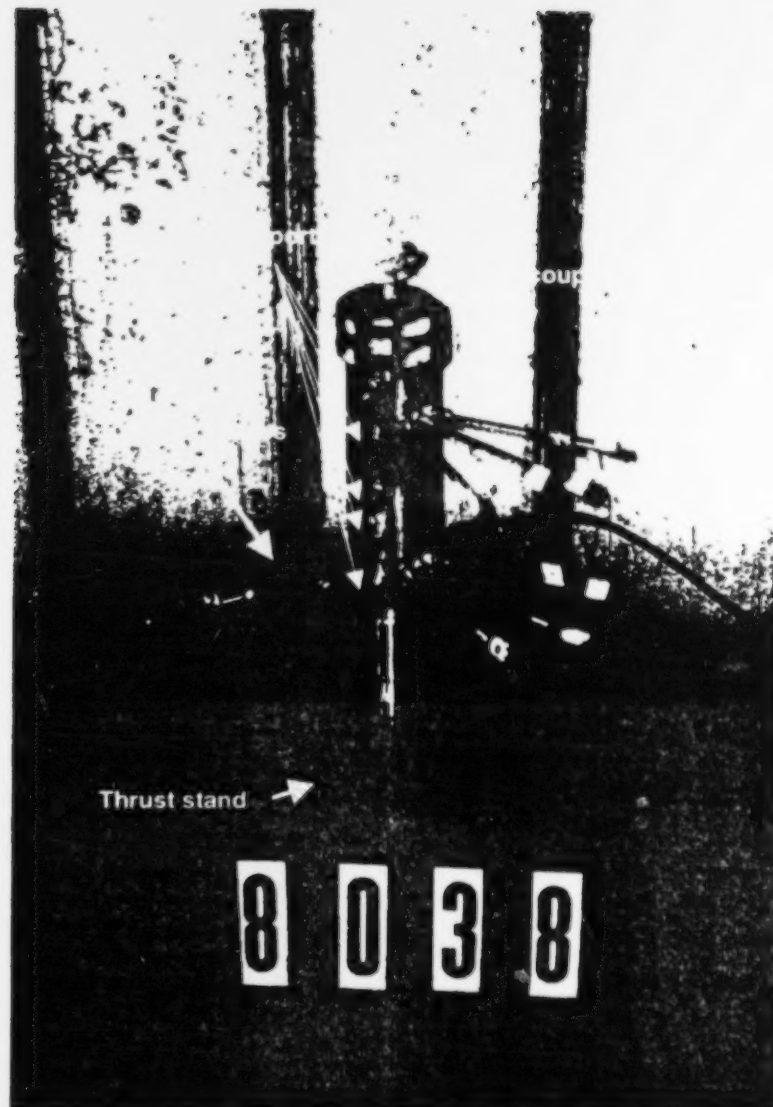


Figure 2 – Test setup of an actual motor on the vertical thrust test stand

4.0 RESULTS AND DISCUSSIONS

4.1 Processing Studies

The options studied were the use of a reduced percentage of fine AP, a lower solid content, a modified mix cycle and an increased binder plasticizer content. Preliminary studies showed promise in terms of burn rate increase, as shown by strand burn rate results in Figure 3. However, problems with very high viscosity during the mix cycle and high end-of-mix viscosities (>10 kP) were experienced.

A new formulation was defined by increasing the plasticizer level from 23.8 to 25% of the binder, at a binder level of 13%. The 45% fine AP content was maintained because of the substantial effect on the burn rate values (Fig. 3). Further studies showed that there was little or no effect on the burning rate at 6.89 MPa ($r_{b(1000)}$) when the plasticizer level was increased to 27.5% or the binder level from 13 to 13.5%.

Progress was made as several mix cycles were evaluated using the torque-meter-equipped mixers with a 45% fine AP content (in blend) and an 87.5% solid content for extended period of mixing under vacuum was found to have a positive effect. Eventually, the mixing cycle was modified by starting at slow speed and mixing until torque value diminished. Only then was the speed ramped-up to the desired value and vacuum applied.

The ground AP storage time was found to have a negative effect on processing. Thus the storage conditions were improved by double-wrapping the AP with desiccant and storing at 60°C until used. Ultra fine aluminum oxide Al_2O_3 (UFAO) to a level of 0.5% of the feed was added to the AP before grinding as an anti-caking agent.

4.2 Burn Rate Studies

The method used to evaluate the burn rate of propellants is well documented in a previous DREV report (Ref. 5). The burning rate measurements were performed in a strand burner and burn-rate motors at an initial propellant temperature of 21°C. Tests were performed at different pressure levels. The measured burn rate at each pressure was plotted on log-log graph. This produced a linear plot of the data points, where the slope of the line represented the pressure exponent of the propellant.

Efforts on propellant formulation were designed to increase the burn rate and lower the pressure exponent, especially at low pressures. This was achievable by one of the following processes: increasing the FeOOH catalyst percentage, using a co-catalyst such as ultra fine aluminum oxide Al_2O_3 (UFAO) (Ref. 7), copper phthalocyanine (CuPht) (Ref. 8) or amorphous boron ($B_{(a)}$) (Ref. 9).

Increasing the FeOOH content to 1.5% resulted in no improvement while addition of 0.5% UFAO increased the $r_{b(1000)}$ by 10-25%. Inclusion of 0.5% CuPht depressed the slope slightly and the use of 90 μ m AP slightly decreased r_b . Unfortunately, all mixes showed higher

end-of-mix viscosities and rate of cure. The worst case was the addition of 0.5% $B_{(a)}$ causing the mix to cure in the mixer.

The formulation first selected for scale-up included 0.5% UFAO and 0.5% CuPht with a binder content that was increased once again to 13.5%. The result was a processable but not castable mix because of a very high end-of-mix viscosity of 40 kP. Work reverted to sub-scale batches eliminating the CuPht. Four sub-scale batches were processed using a modified mixing cycle: 1% FeOOH, 1.5% FeOOH, 1% FeOOH + extra 0.3% UFAO and a repeat with 1% FeOOH but with new lots of bonding & curing agents. Results showed easier to process propellant with low end-of-mix viscosities (5-7.5 kP). Strand burn rate results are illustrated in Figure 4. The increased FeOOH percentage resulted in higher rate of cure and no improvement in $r_{b(0.00)}$. Extra UFAO did not increase burn rates. Overall, burning rates of 33—35 mm/s at 6.89 MPa were obtained with a rather high pressure exponent in the 0.76-78 range between 3.45 and 13.79 MPa pressure range (500-2000 psia).

For the second attempt, the chosen baseline formulation (cf. 3240 and 3243 in Fig. 4) included 1% FeOOH and 13.5% binder and the modified mixing cycle was used. The mix was processable and castable and showed normal initial rate of cure. A second sub-scale batch was processed and burn rate motors were cast to assess the ballistic behavior of the propellant formulation in actual motors. Nevertheless, the rather high exponent, confirmed by the burn rate motors, was considered unacceptable. This made casting of booster motors impossible without further modifications to the formulation.

Further investigation work to explore solution avenues led to a few more variations on the baseline experimental formulation. Burn rate results are illustrated in Figure 5. The outcome of this last round of experiments was

- the re-introduction in the formulation of CuPht as a co-catalyst. It was clearly identified in the study as the only factor reducing the pressure slope,
- an increase in the binder content to 15% to improve processing since CuPht is also a cure rate catalyst and tends to negatively alter pot-life,
- and, the replacement of 200 μ m AP by 400 μ m was found to slightly increase the burn rate.

Final burn rate evaluation of the selected formulation, both on sub-scale and full-scale mixes, using strands and burn-rate motors, revealed more comfortable pressure exponent values (0.445-0.452) but, as illustrated in Figure 6, burn-rates are only slightly higher (5-15%) than the reference propellant formulation (HBR-130) from which it originates. The final formulation of 3269 yielded an acceptable pressure exponent of 0.412 using both the strand and burn-rate motors for validation. Processing properties were marginal but sufficient to allow casting of experimental motors.

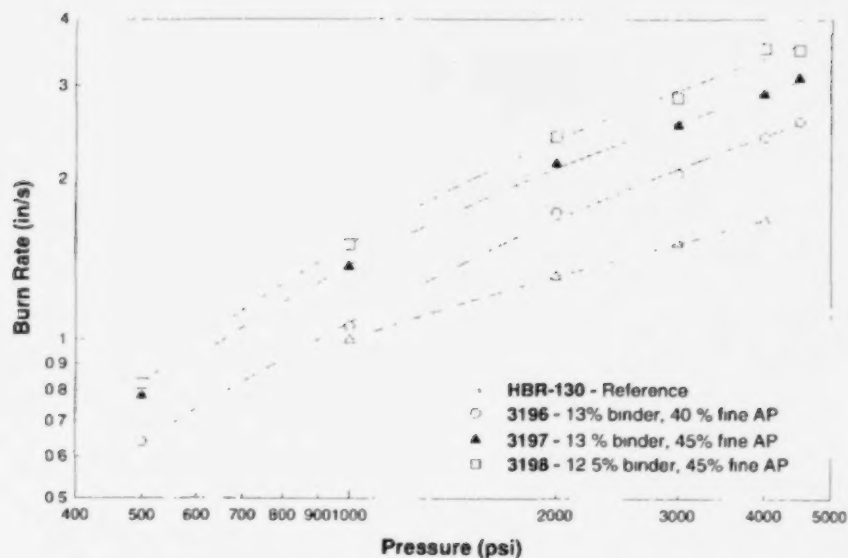


Figure 3 - Preliminary studies, strand burn rate results.

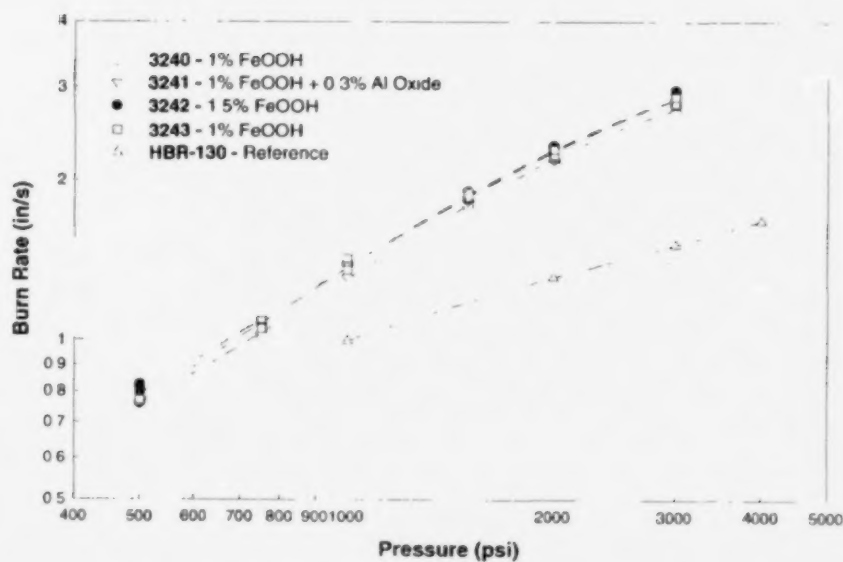


Figure 4 - Baseline propellant formulation, strand burn rate results

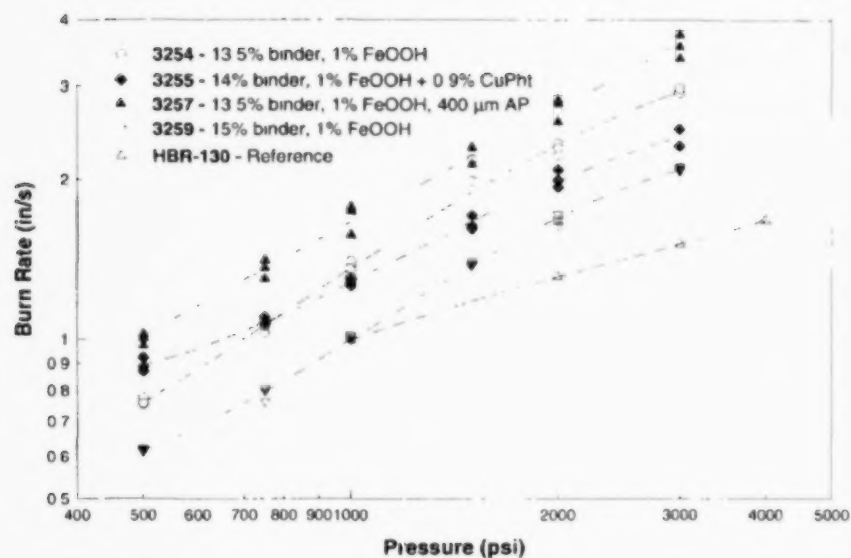


Figure 5 – Variations of baseline propellant formulation, strand burn rate results

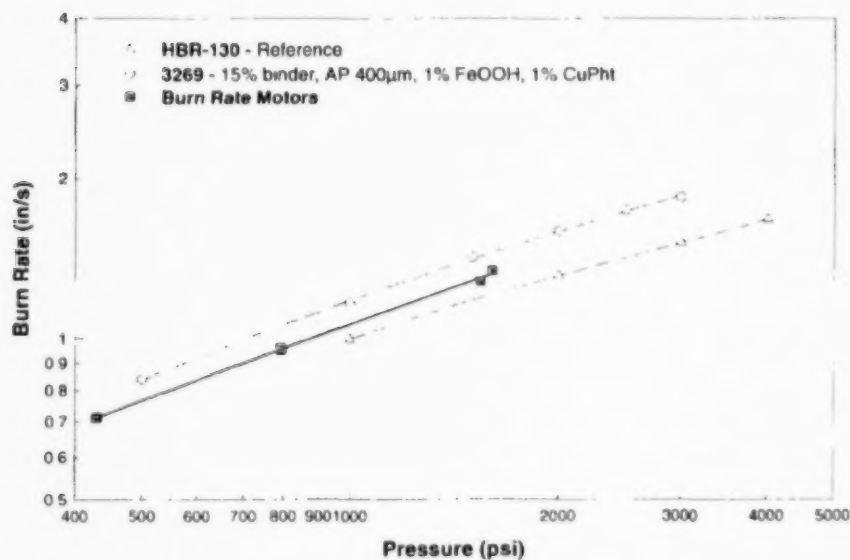


Figure 6 – Final formulation, burn rate results

4.3 Motor Firings

Table 2 shows the measured experimental values for the test motors fired. These values were obtained by a data reduction program for rocket engine development that treats both pressure and thrust curves. The method is described in Ref. 10. It is first observed that, for L/D of 5 and 6, chuffing was present throughout the burning. It starts early upon ignition and continues until the propellant has completely burned away. An oscillation is observed at low pressures during the firing of these motors, as shown in the pressure traces in Figures 7 and 8. This nonacoustic instability is attributed to the rapid decay of the pressure due to the small chamber volume and the small critical initial L^* (characteristic chamber length) associated with this particular propellant configuration (Ref. 11). The L^* is a parameter useful for determining the limiting attributes (throat area, chamber pressure, etc.) of a particular propellant combination. The L^* is calculated by the ratio of the initial chamber volume to the initial throat area. The value calculated for this propellant and configuration is 0.4 m. Therefore, any value of $L^* < 0.4$ would result in a stable combustion, which is shown clearly for motors having an L/D of 7 to 11 in Figures 9 to 13.

Table 2 - Experimental results for test motors

L/D	PEAK HEAD END PRESSURE (kPa)	AVERAGE PRESSURE (kPa)	BURN TIME (S)	AVERAGE THRUST (N)	ISP (s)	L^* (m)	OBSERVATION
5	5957					0.32	Chuffing during 10 s
6	9885					0.38	Chuffing during 10 s
7	9878	1696	1.736	1895	164	0.44	
8	9442	2358	1.517	2647	171	0.51	
9	6935	2613	1.214	3447	169	0.57	
10	8132	3144	1.14	3919	163	0.64	
11	7026	3447	0.954	5022	167	0.70	

Static pressure profiles in Figures 7 to 13 along the length, from head end to nozzle end, of the propellant showed variations in peak pressures. It is apparent that the pressure during burn decreases along the length of the bore. This trend is an indication of the eroding and varying choke area of the nozzle. Table 3 also shows that the initial peak pressures decreases at the nozzle end compared with that at the head end. This decrease can be as much as 50% when comparing the peak pressure at the head to the nozzle end. This would be an indication of the varying choke location along the length of the motor. Also observed is the decrease in burn time as L/D increases. This implies that the increase in chamber volume increases surface area burn and, consequently, increase the average chamber pressure.

Figure 14 shows the experimental thrust measured as a function of L/D. For motors having a L/D of 5 and 6 the thrust was not obtained due to the instability discussed earlier in the report. The thrust curves show a reasonable plateau with a small decay during burn. The slope of the decay is constant for all L/D. An increase in the length-to-diameter ratio, which also correlates to increased propellant mass of the booster and increased mass flow due to a higher burn

surface, is found to effectively increase thrust. The curves show constant trends, except that at a motor L/D of 7, an oscillation near the end of burn is present as was also found in the pressure curves. This may mean an unstable combustion in the motor. The average thrust of these motors varies from 1895 kN for L/D=7 to 5022 kN for L/D=11 and is also shown in Table 2. It is also noticed in both the pressure profiles and the thrust curves that the duration of the burn is inversely proportional to L/D, that is, the burn time decreases as the L/D increases. One noticeable attribute of the motor firings is that the I_{sp} seems to be similar for L/D of 7 to 11. The I_{sp} is interpreted by the following equation

$$I_{sp} = \frac{\int_0^{t_b} F dt}{m_p}$$

where F is the measured time-averaged thrust, t_b is the measured burn time and m_p is the mass of the propellant. Since the thrust increases while the burn time decreases, the I_{sp} becomes a constant value for these concentric bore motors. The measured average I_{sp} for these motors is 166 s

Table 3 - Variation of the head and nozzle end peak static pressure

L/D	PEAK HEAD END PRESSURE (KPA)	PEAK NOZZLE END PRESSURE (KPA)	% DIFF
5	5957	2615	56.1
6	9885	5064	48.8
7	9878	4414	55.3
8	9442	5438	42.4
9	6935	4497	35.1
10	8132	4836	40.5
11	7026	4468	36.4

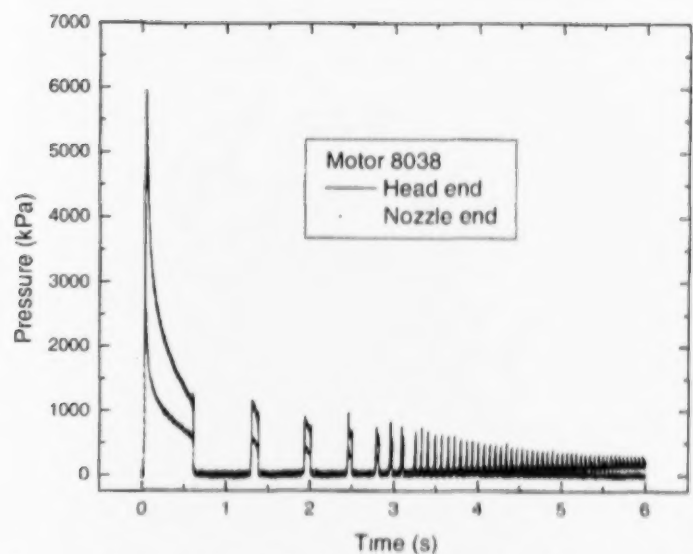


Figure 7 – Pressure history of motor 8038 along its length, having an L/D=5

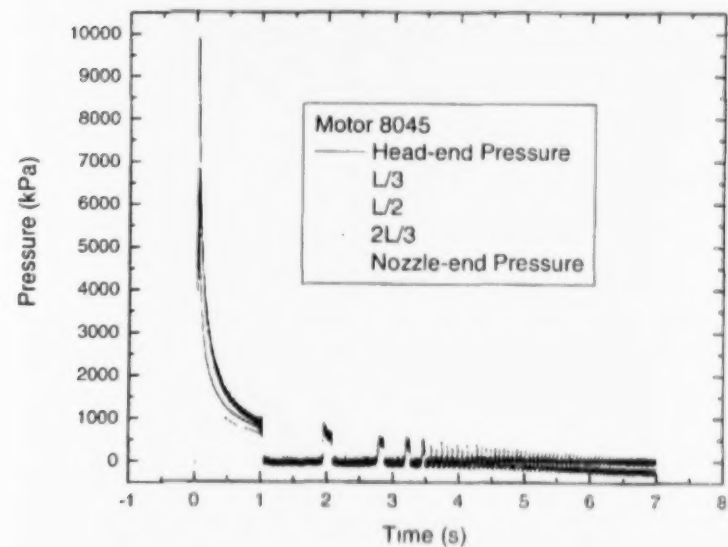


Figure 8 – Pressure history of motor 8045 along its length, having an L/D=6

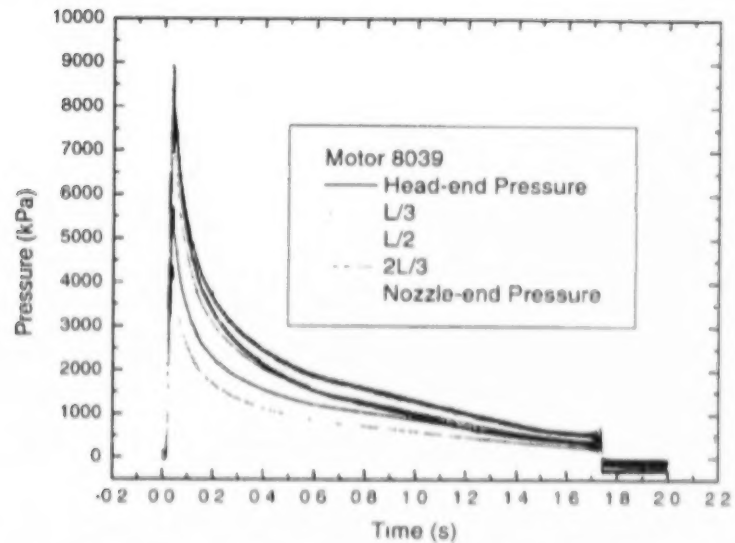


Figure 9 – Pressure history of motor 8039 along its length, having a $L/D=7$

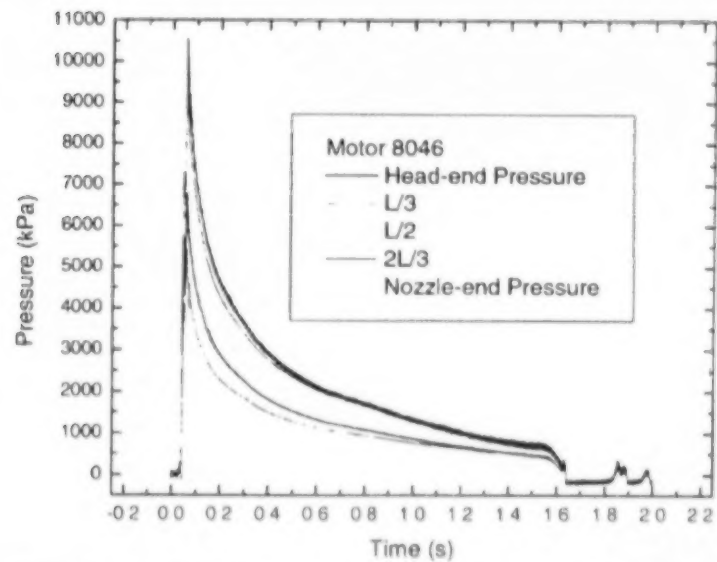


Figure 10 – Pressure history of motor 8046 along its length, having an $L/D=8$

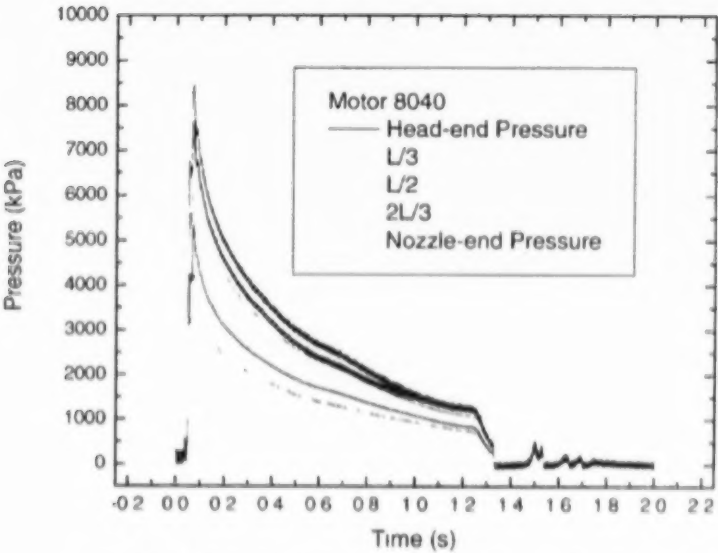


Figure 11 – Pressure history of motor 8040 along its length, having an L/D=9

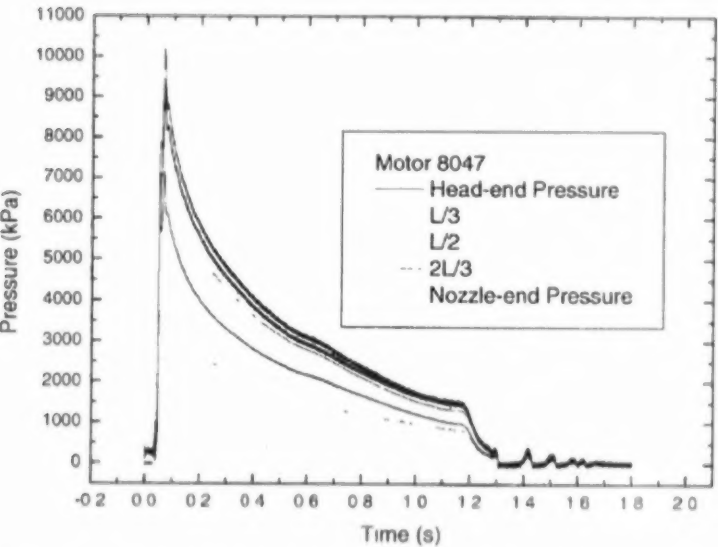


Figure 12 – Pressure history of motor 8047 along its length, having an L/D=10

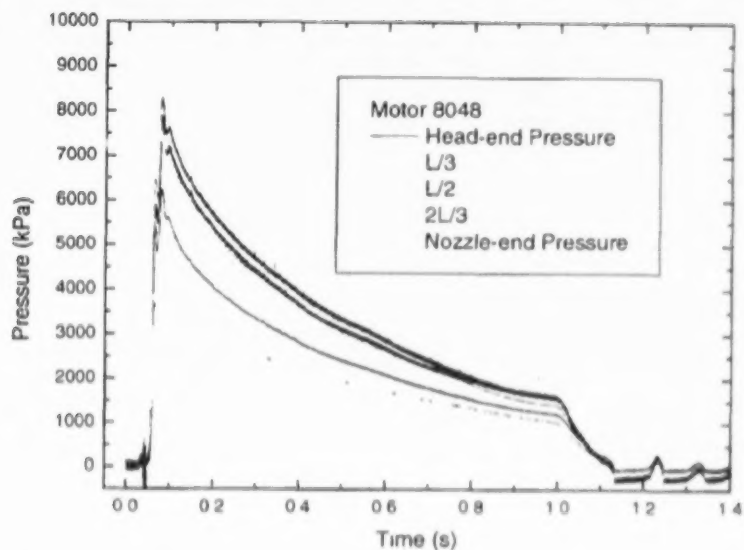


Figure 13 - Pressure history of motor 8048 along its length, having an $L/D=11$

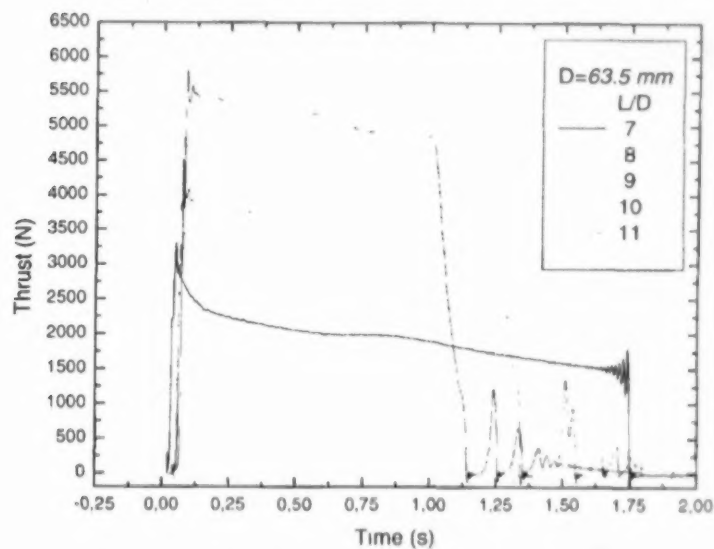


Figure 14 - Thrust curves of the various motor firings

By examining the pressure profiles, one can deduce that given the burning rate laws, the burn rate at the head end should be greater than that at the nozzle end. This is further shown in the temperature profiles in the figures in Annex B, where the thermocouple measurements embedded within the propellant show the arrival time of the burn front at both the head and nozzle end these act start-stop time gauges for burn front arrival time. From these figures, it is possible to calculate the flame front arrival times at various web depth. The results are graphically shown in Figures 15 to 21 for each of the motors tested. The figures illustrate that for all motors, the burning time is much shorter or burn rate is much higher at the head end than that of the nozzle. Even when the motor is chuffing, (motors 8038 and 8045) the trend is present but it is not as apparent as the burn rates of the motors that burned clean. This would indicate an initial higher rate of burn nearer to the head end due to local crossflow velocities and more constant pressure. This, subsequently, characterizes a larger gas mass-flow early in the burn (Ref. 2). The burning mechanism is believed to be caused by an increase in the gas to solid heat feedback introduced by the increase in transport coefficients and by the turbulence-enhanced mixing (Refs 12 and 13). This burning mechanism is characterized as erosive burning. Usually in nozzled motors the erosive burning occurs further downstream of the motor just ahead of the nozzle converging section. However in nozzleless motors where the bore acts like the nozzle, the increased burn happens upstream from where the flow is choked and that flow can be choked anywhere along the length of the propellant at any time during the burn. King (Ref. 14) demonstrated that erosion effects are dictated by the ratio of the head to nozzle burn rate ratio. King developed a burning rate correlation that illustrates erosive burning for nozzleless motors. It can be shown that by using the correlation, the erosivity constant increases with L/D of the motors. The derived equation is given by

$$r_{\text{aft}} / r_{\text{fore}} = (k_2 + 1) / (\gamma + 1)^n$$

where r_{aft} is the burn rate at the nozzle end and r_{fore} is the burn rate at the head end, $\gamma=1.2$ (product specific heat ratio), $n=0.412$ (propellant burning rate pressure exponent) found by the burn rate tests and k_2 is the erosivity constant. The erosivity constant is a measure of erosive burning, where a value of zero means no erosion. The data observed by the thermocouples was used to calculate the mean burn rates at both the head and nozzle ends. Table 4 shows the respective amounts of erosive burning for each of the L/D ratios. It can be seen as one increases the L/D, the burn rate ratio increases and indicates an increase in the erosivity of the motor. It can also be seen that at L/D=11 the erosivity was found to be lower. This measured value of $r_{\text{aft}}/r_{\text{fore}}$ can be erroneous due to a thermocouple failure at the nozzle end which can easily be seen in Figure 21.

Nozzleless motors require that the flow within the bore of the propellant grain accelerate so that conditions become sonic nearer to the aft or nozzle end of the propellant. The high velocities across the grain could result in substantial erosive burning and consequently locally increase the burning rate of the propellant at that location (Ref.2). The erosion, which occurs principally at the choke point and varies along the length of the motor during burning, causes the chamber pressure to vary. These effects are further enhanced by the presence of high cross flow velocities at the choke area.

Table 4 Erosivity constant at each L/D

L/D	r_{at}/r_{ore}	k_2
7	0.756	0.046
8	0.821	0.136
9	0.842	0.165
10	0.910	0.26
11	0.803	0.112

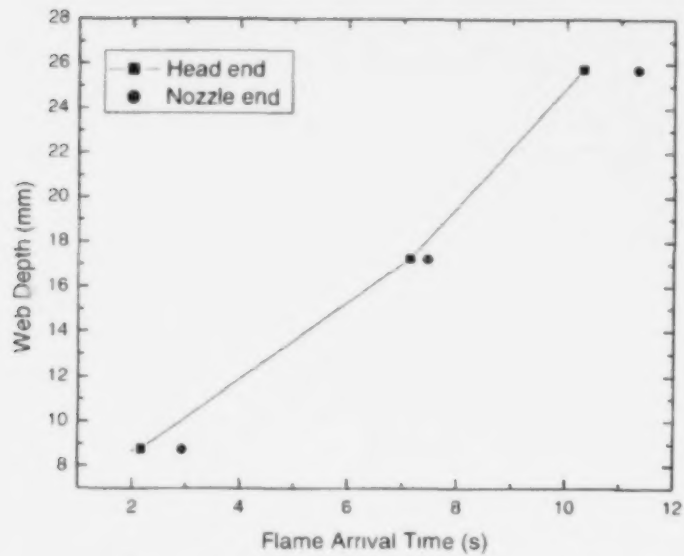


Figure 15 - Flame arrival for motor 8038 and L/D=5

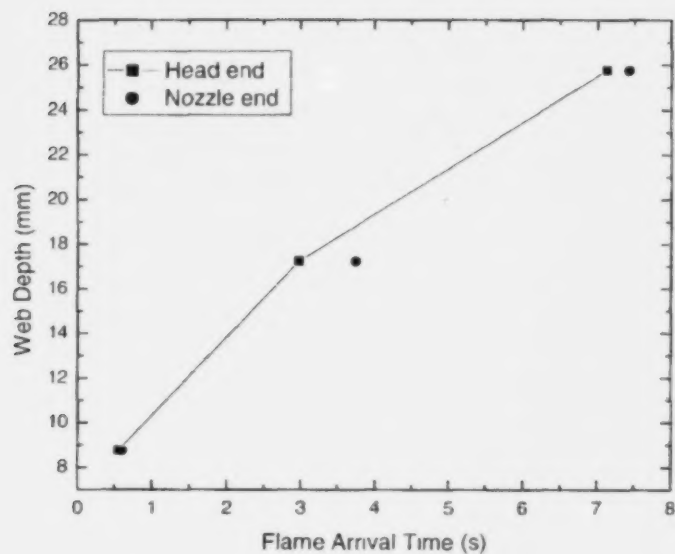


Figure 16 – Flame arrival for motor 8045 and L/D=6

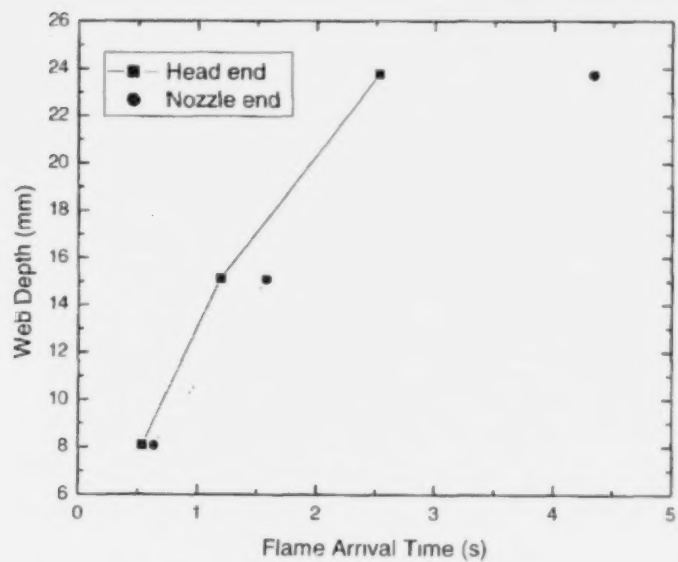


Figure 17 – Flame arrival for motor 8039 and L/D=7

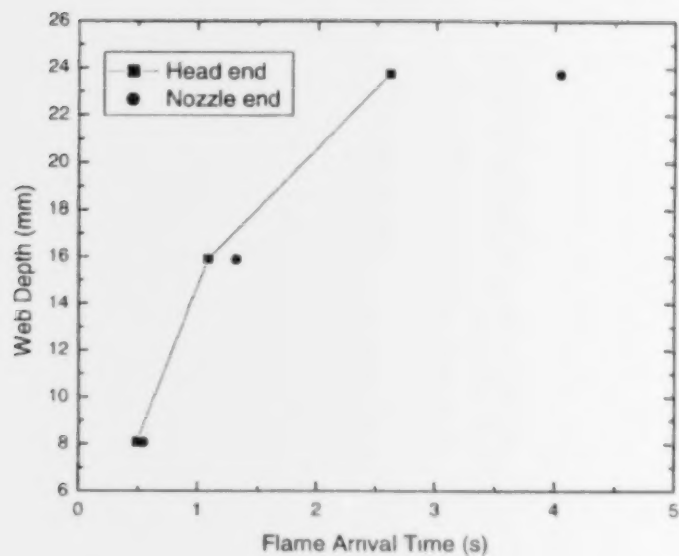


Figure 18 – Flame arrival for motor 8046 and $L/D=8$

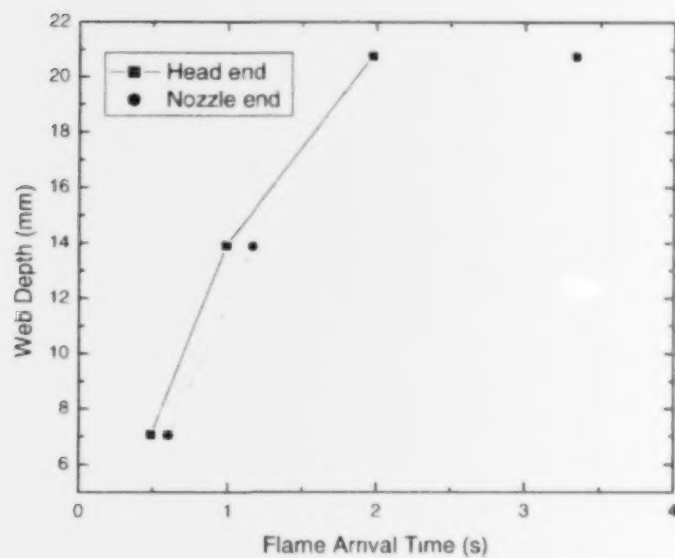


Figure 19 – Flame arrival for motor 8040 and $L/D=9$

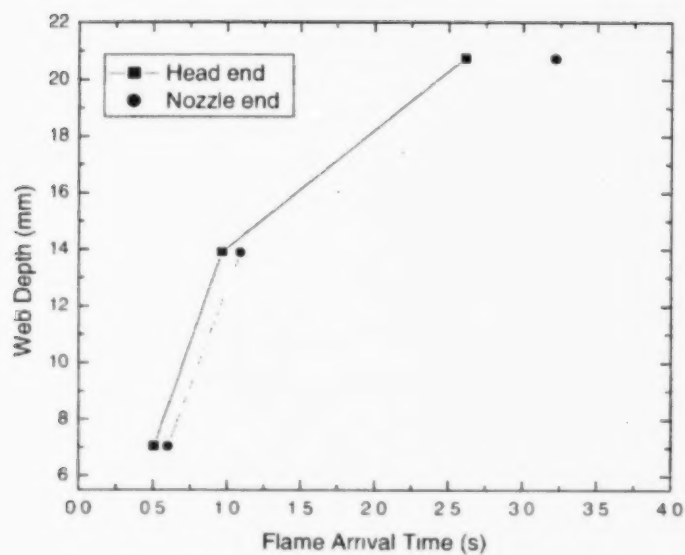


Figure 20 - Flame arrival for motor 8047 and $L/D=10$

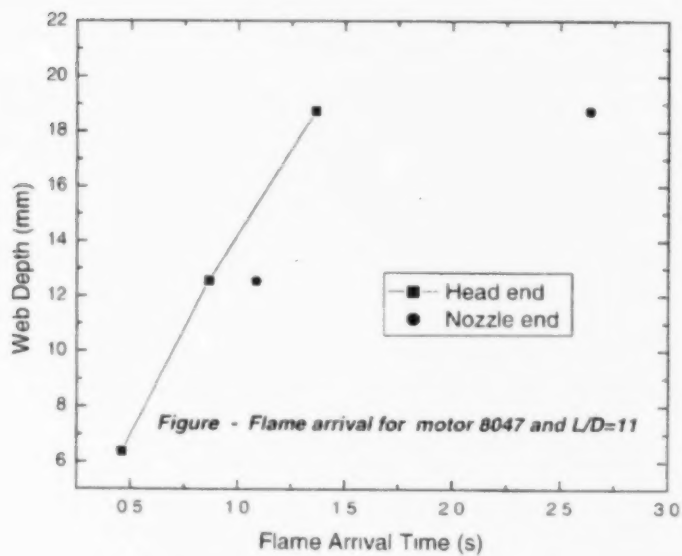


Figure 21 - Flame arrival for motor 8048 and $L/D=11$

It is also possible to correlate the head end pressure to a relationship with L/D_i , where D_i is the initial bore diameter of the propellant. Table 5 depicts the data for peak head end pressure and the ratio of the L/D_i . By observing the trend it is possible to denote that

$p_{\max} \propto (L/D_i)^{(1/(1-n))}$ (Ref. 1), where $p_{\max}$ is the head end pressure. It is reasonable to also state that the head end pressure is a function of the characteristic velocity (c^*), the propellant density (ρ) and of the constant (a) in the burning rate law given by $r = a P^n$. Figure 22 shows a logarithmic plot of the $p_{\max}$ vs the $4L/D_i$, which in physical terms represents the burning-to-throat area ratio

Table 5 – Head end pressure and L/D_i

L/D	PEAK HEAD END PRESSURE (KPA)	L/D_i
5	5957*	26.45
6	9885*	31.75
7	9878	27.78
8	9442	31.75
9	6935	25.97
10	8132	28.86
11	7026	26.86

* these are omitted in the plot due to incomplete/unstable combustion

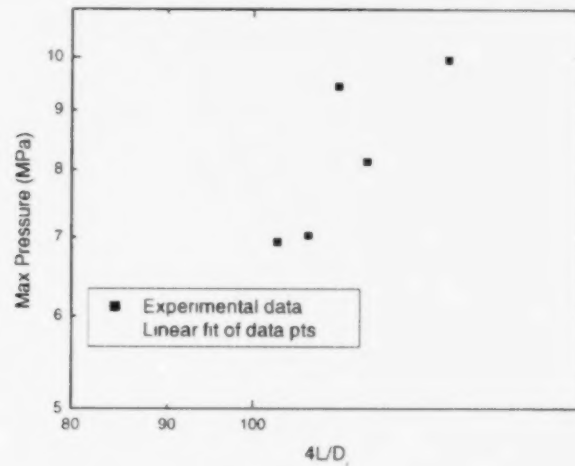


Figure 22 – Plot of pressure and data correlation

A linear fit of the data gave a slope of 1.709. This value is the term $1/(1-n)$. Therefore, n was found to be equal to 0.415, which correlates quite well with the pressure exponent of the propellant burn rate data obtained by both the strand and 2X4 motors. The pressure can then be represented by the equation of the line given by

$$p_{\max} = \left[\rho \cdot a \cdot c^* \cdot \frac{4L}{D_t} \right]^{\frac{1}{1-n}}$$

which is a reduced form of the standard ballistic formula.

4.4 Model Predictions and Comparisons

A comparison was made between the experimental and the 1-D model calculations of pressure and thrust. By comparing the experimental thrust results in Figure 14 with those calculated and shown in Figure 23, some similar trends are observed. Both experimental and prediction values show a rise to an initial thrust value that tries to remain constant during the rest of the burn. The measured thrust shows an initial peak followed by slow decay, whereas the predicted values show an increasing asymptotic trend until the end of burn. The trend exhibited by the measured thrust-time curve seems to indicate erosive burning due to a decreasing combustion mass flow. Since the propellant is consumed more rapidly during the early burning, the mass flow reduces and thus decreases the thrust at the end of burn. In both the thrust cases increases with the L/D. By comparing the average thrust values in Table 2 to the calculated ones in Table 6, it is observed that the measured values are 10 to 30% lower than the predicted values. This difference can be attributed to the simplistic nature of the model (Ref.6) along with errors in the experimental instrumentation estimated to be in the order of 2-3%. The comparison of the average pressures between the measured and the calculated values, yields differences, as much as 40 % higher for the predicted values. However, as shown in Figures 24 and 25, the trends for pressure over the burn time are similar and show a decaying development with burn time. The average pressures also increases with the L/D for both the model and experimental values. The duration of the burn or burn time of the predicted values demonstrates good agreement with those measured and are shown in Table 2. It is shown that the burn time also decreases with L/D and the burn time difference between the measured and predicted values is less than 10%. It was also noted that specific impulses measured on these motors averaged to 166 s whereby the model calculated an average of 187 s; a difference of about 12%. Therefore, the model predictions, for this particular propellant with a simple bore configuration, overestimate the performance parameters. This is, as stated earlier in the paragraph, mostly due to the limitation of the model. It has become evident that an erosion model should be implemented for the performance prediction to be more realistic. However, it is worth noting that the trends between the model and experiments are similar.

Table 6 - Model predictions

L/D	PEAK HEAD END PRESSURE (KPA)	BURN TIME (S)	AVERAGE PRESSURE (kPa)	AVERAGE THRUST (N)	ISP
7	6120	1 589	2386	2487	184
8	13800	1 484	3045	3078	186
9	9710	1 239	3063	3769	187
10	11600	1 125	3640	4720	187
11	14000	1 031	4397	5765	188

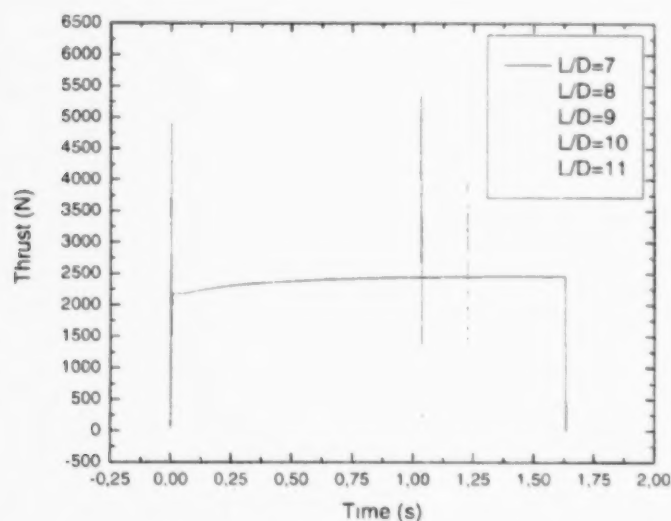


Figure 23 - Thrust curves of the prediction model at different L/D

A good indication of the model's ability to predict the physical parameters is to compare the experimental pressure ratio along the length of the motor. The experimental static pressure ratios were plotted for various times during the burn at the five Kistler locations along the length. To simplify the illustration of the analysis, the plots depict two L/D values. Figures 26 and 27 show the values obtained for motors with L/D of 7 and 11, respectively. The one-dimensional prediction model, which assumes a uniform burn rate along the grain length, is also plotted in the figures. The agreement of the prediction curve and the experimental data for the pressure ratio is not very evident at an L/D of 7. The experimental data shows a peak pressure ratio at the second gauge location and decreases along the rest of the length. The data show a Gaussian relation, as illustrated by the red curve, whereas the model depicts a Boltzman relation. This suggests that a second logarithmic term is lacking in the prediction.

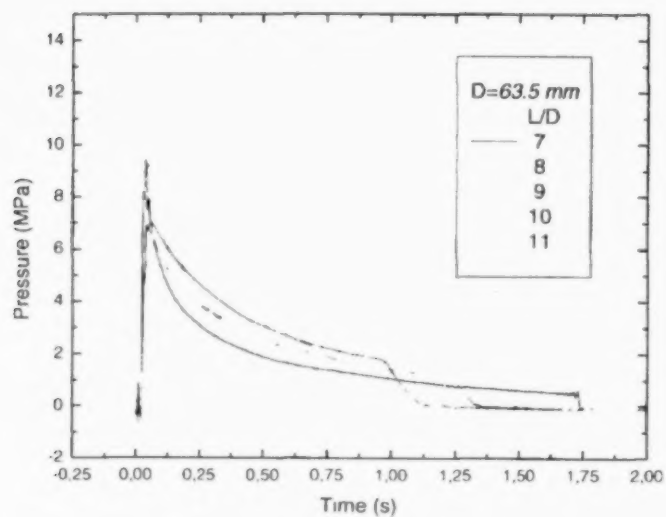


Figure 24 – Experimental head-end (Baldwin gauge) pressure profiles for different L/D

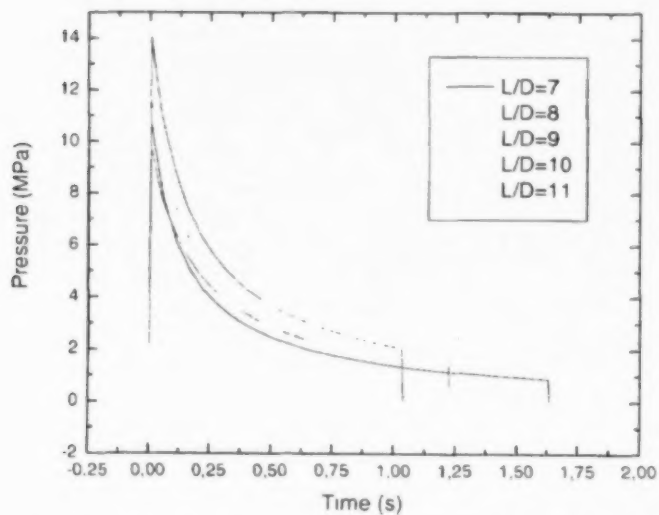


Figure 25 – Prediction model pressure profiles for different L/D

model, which would characterize the eroding and irregular burning on the propellant surface. However, it was found that as the L/D increased, the curves become comparable. At an L/D of 11 the model and experiments show reasonable agreement. This is not surprising because it was found that the burning ratio from aft to fore approaches unity as the L/D increases. Therefore, the group of pressure ratios reveal that secondary effects greatly influence the burn rate at lower L/D ratios. The model does not incorporate these kinds of effects.

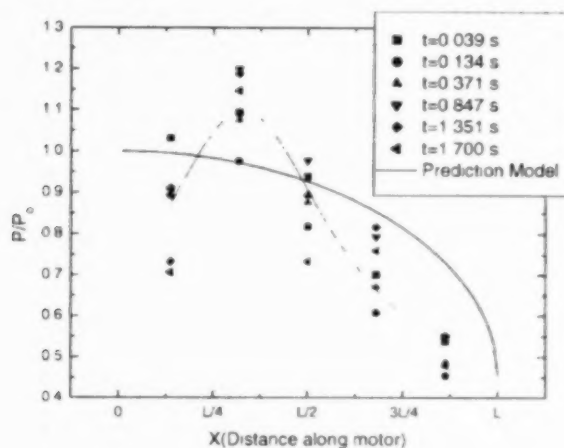


Figure 26 - Pressure ratio plot for motor having an $L/D=7$

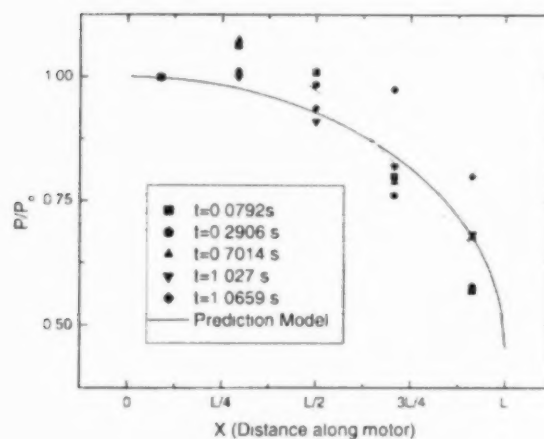


Figure 27 - Pressure ratio plot for motor having an $L/D=11$

The model does demonstrate that it is a reasonable approximation tool for the nozzleless motor performance. The assumptions made for the model are basic and valid for situations in which a first estimate of the performance parameters and trends are required for design purposes. However, as was evident in the previous observations, a more detailed 2-D theory supporting erosive burning effects and non-uniform burn rates along the axial surface length would more precisely predict the ballistic performance of the nozzleless booster.

5.0 CONCLUSION

The development of a high burn rate propellant for use in a nozzleless booster for ducted rocket and ramjet applications yielded a propellant formulation that included a coarser grade of coarse AP particle size and the addition of FeOOH and CuPht co-catalysts to enhance burn rate values. The presence of the CuPht catalyst lowered the slope of the pressure exponent and allowed a more consistent burn rate over a larger pressure regime. The consistent burn rate, for a cylindrical bore nozzleless motor, would increase thrust or keep it constant during burn. Although the addition of finer 3.7 μm AP gave a marked increase in burn rate, it was found to also increase the slope of the pressure exponent. This was found to be unacceptable for applications of this kind. A compromise formulation was found where a balanced FeOOH/CuPht co-catalyst and an increased binder content for improved processing gave an increase of 15% in burn rate at 6.89 MPa over a previous HTPB/AP formulation and an acceptable pressure exponent of 0.412.

This work included a study of the performance effects on the developed propellant in actual rocket configurations where the propellant length-to-diameter ratio was altered for each test. The ballistic performance showed an increase in thrust with increased L/D for similar head end pressures. It was also noted that the static pressure along the length of the grain decreased and that the burn rate was non-uniform for all motors fired. This indicated that the burn rate was location and burn time dependent. This effect was also confirmed by thermocouple measurements at both the aft and head-end of the motor. The irregular burn rate across the surface of the propellant denoted some erosive burning. It was also found that the experimental pressure and the L/D could be correlated using an equation incorporating the propellant burning characteristics.

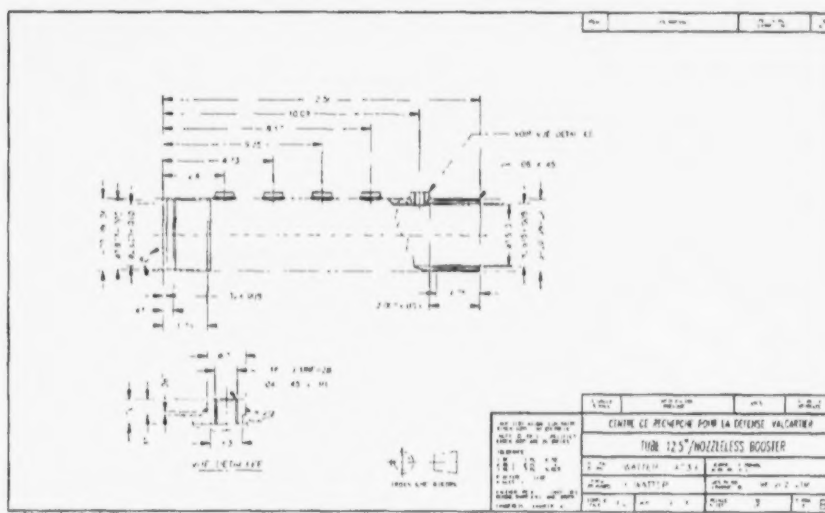
A comparison between the predictions from a 1-D performance model and the test results showed that the model produced comparable results. It can be used as an initial tool for calculating the ballistic performance of nozzleless motors. However, due to the simple 1-D formulation assumptions, the model lacks the details that would allow it more closely predict the performance of a nozzleless motor. A 2D model, with erosive burning, initiation and tail-off features should be considered for future prediction capabilities.

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Annex A



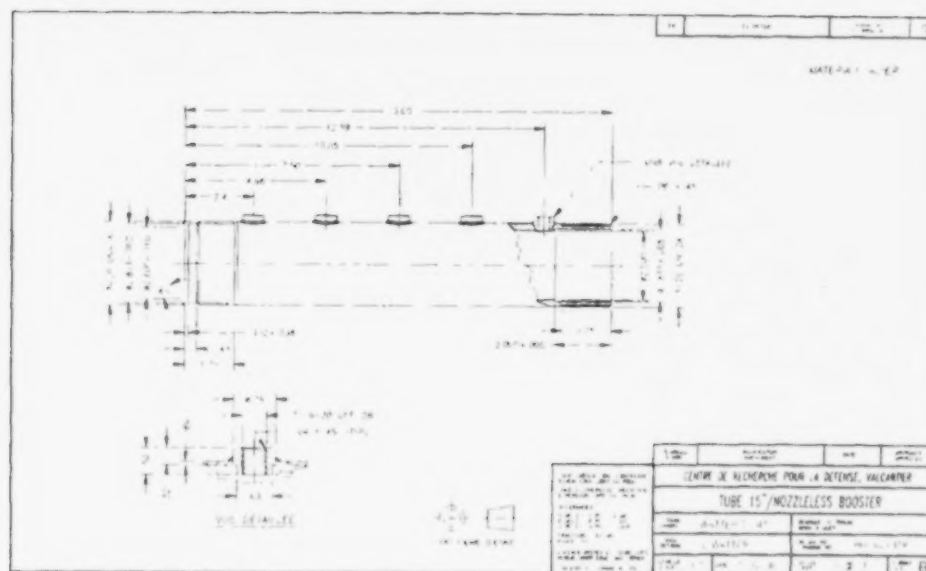


Figure A2. Schematic of the 381 mm long casing showing location of pressure ports

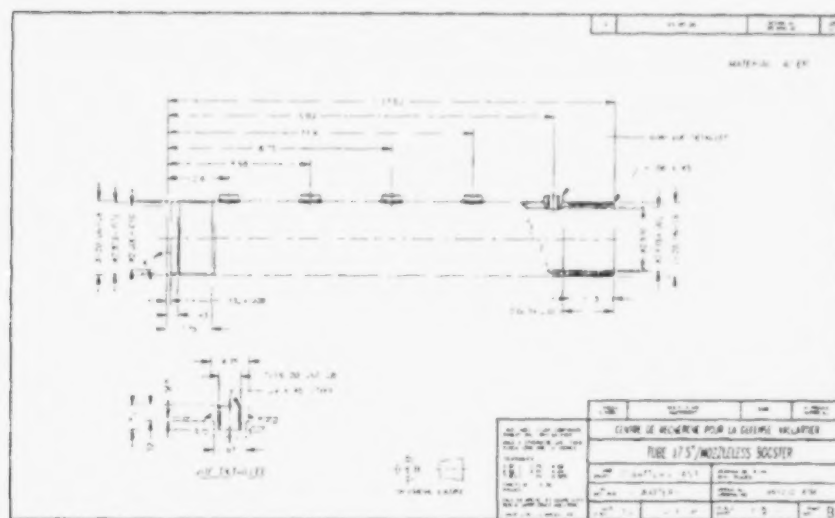


Figure A3. Schematic of the 444.5 mm long casing showing location of pressure ports

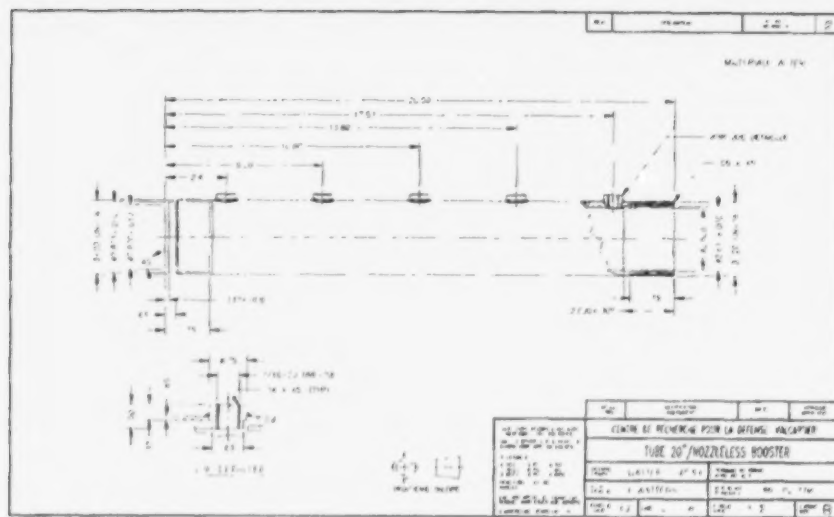


Figure A4. Schematic of the 508 mm long casing showing location of pressure ports

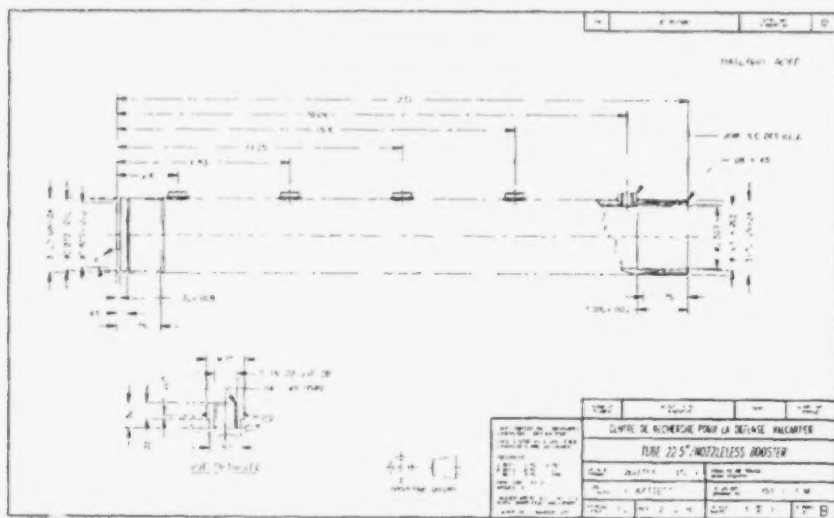


Figure A5. Schematic of the 517.5 mm long casing showing location of pressure ports



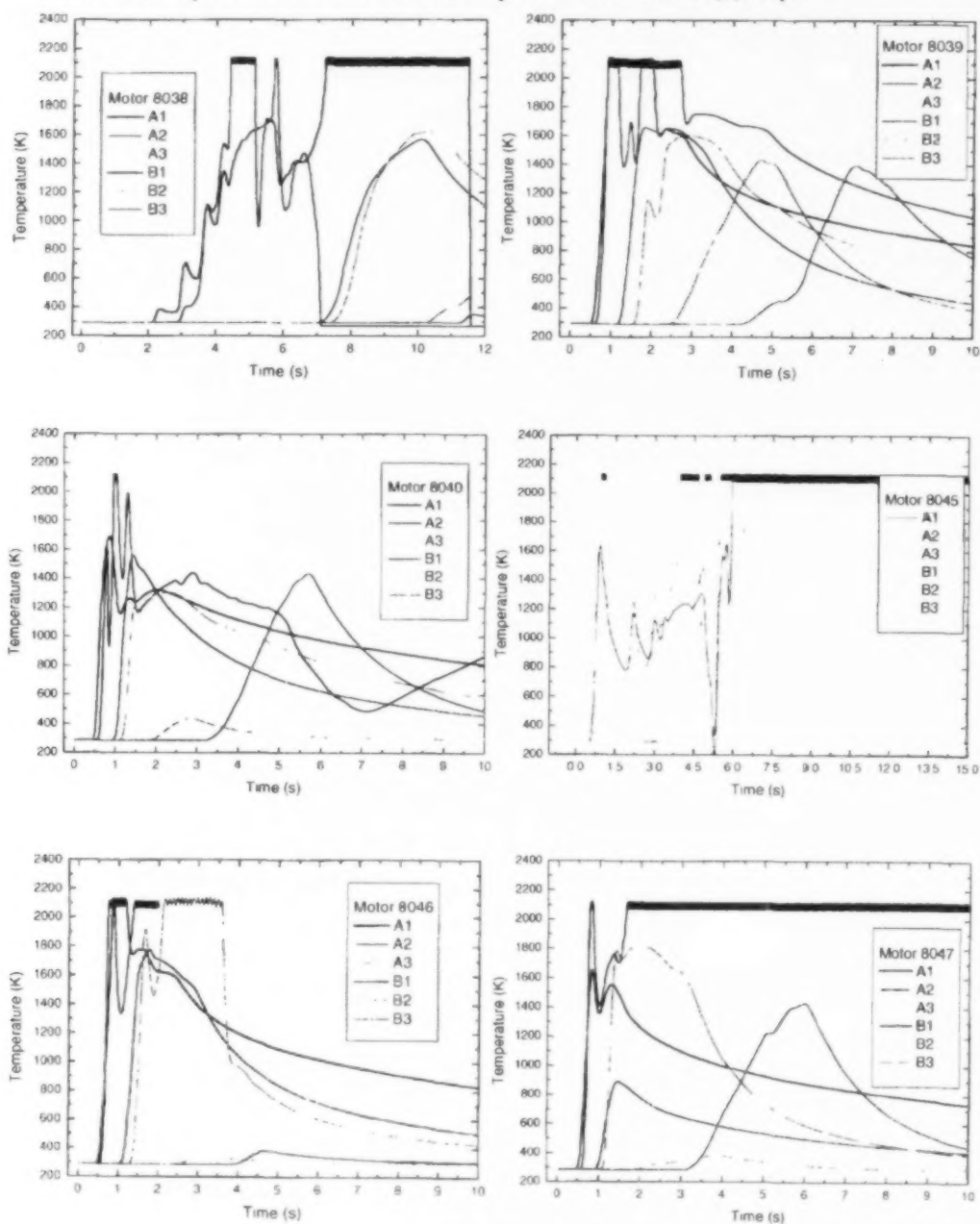
Figure A6. Schematic of the 635 mm long casing showing location of pressure ports



Figure A7. Schematic of the 698.5 mm long casing showing location of pressure ports

Annex B – Thermocouple results of nozzleless motors

A1 A2 A3 represents Head end; B1 B2 B3 represents Nozzle end; 1,2,3 depth



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A formulation based on HTPB/AP propellant with FEOOH/CuPht co-catalysts was developed as a potential candidate for use as a nozzleless booster propellant. This propellant exhibited qualities of high burn rate and good combustion stability with high mass flow necessary to cause exhaust gases to choke downstream in a cylindrical grain. Another consideration was that the pressure exponent was low enough to give a more uniform burn rate across a larger pressure regime. The propellant exhibited a burn rate of 30 mm/s at 6.89 MPa and a pressure exponent of 0.412. Tests of experimental motors have shown that the pressure and thrust are dependent on the ratio of the motors' length to diameter (L/D). A prediction model yielded similar results as those obtained experimentally. However, it was shown that erosion effects and non-uniform burn rates along the axial length affect the overall ballistic performance and that the precision model lacks these effects. The 1-D model, in most part, overestimates the performance by omitting the 2D complexity of the problem.

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